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PRELIMINARY INVESTIGATION OF AN UNDERWATER RAMJET POWERED BY COMPRESSED AIR

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SUMMARY

Part I contains the results of a preliminary experimental investigation of a particular design of an underwater ramjet or hydroduct powered by compressed air. The hydroduct is a propulsion device in which the energy of an expanding gas imparts additional momentum to a stream of water through mixing. The hydroduct model had a fineness ratio of 5.9, a maximum diameter of 3.2 inches, and a ratio of inlet area to frontal area of 0.32. The model was towed at a depth of 1 inch at forward speeds between 20 and 60 feet per second for airflow rates from 0.1 to 0.3 pound per second. Longitudinal force and pressures at the inlet and in the mixing chamber were determined.

The hydroduct produced a positive thrust-minus-drag force at every test speed. The force and pressure coefficients were functions primarily of the ratio of weight airflow to free-stream velocity. The maximum propulsive efficiency based on the net internal thrust and an isothermal expansion of the air was approximately 53 percent at a thrust coefficient of 0.10. The performance of the test model may have been influenced by choking of the exit flow.

Part II is a theoretical development of an underwater ramjet using air as "fuel." The basic assumption of the theoretical analysis is that a mixture of water and air can be treated as a compressible gas. More information on the properties of air-water mixtures is required to confirm this assumption or to suggest another approach. A method is suggested from which a more complete theoretical development, with the effects of choking included, may be obtained. An exploratory computation, in which this suggested method was used, indicated that the effect of choked flow on the thrust coefficient was minor.

INTRODUCTION

The basic concept of the hydroduct or underwater ramjet consists of the production of thrust by the transfer of the potential energy of

a compressed gas to a flowing liquid through a mixing process. Water entering the hydroduct inlet is diffused to a high static pressure and mixed with the expanding gas; the mixture is then expanded through the duct exit with a resultant increase in total momentum. A theoretical analysis of the hydroduct is presented in reference 1; reference 2 contains some results of tests of a hydroduct.

The hydroduct is of potential interest for the propulsion of surface craft designed for high speeds because of certain apparent advantages over conventional propulsion systems. Because of the limitations of
planing craft in rough water, any significant increase in the speed of L
practical surface craft must be accomplished with hydrofoils. It has 1
been proposed that the hydroduct and the ducts to supply air to it could 2
be contained within the hydrofoils and supporting struts. Also, it has 4
been conjectured that such a system could have less frontal area, a 9
higher speed capability, and lower noise level than supercavitating gear-driven propellers; however, an evaluation is not possible at the present time because of insufficient hydroduct data. Because no static thrust is developed by the hydroduct, the system must include an auxiliary propulsion device.

Because of the apparent advantages of the hydrofoil-hydroduct system for propulsion of high-speed surface craft, a preliminary investigation of a simple axisymmetric air-powered hydroduct has been made in Langley tank no. 1 (this facility has recently been transferred to the David Taylor Model Basin) to evaluate the thrust-producing capability of such a device. The hydroduct was towed at shallow draft at speeds between 20 and 60 feet per second with variations in airflow rate from 0.1 to 0.3 pound per second. Measurements were made of the towing force and pressures at the inlet and maximum-area section. The results of these tests are described in part I of this report.

As a result of the preliminary experimental investigation it was apparent that a theory was needed to guide future work. Accordingly, an analysis was made of the ideal performance of the air-powered hydroduct. This analysis is presented in part II.

SYMBOLS

A area, sq ft

A_f frontal area of body, 0.0559 sq ft

a speed of sound, ft/sec

C_A axial-force coefficient, $\dfrac{F_A}{\frac{1}{2}\rho_w V_\infty{}^2 A_f}$

C_p pressure coefficient, $\dfrac{p - p_\infty}{\frac{1}{2}\rho_w V_\infty{}^2}$

$C_{T,c}$ thrust coefficient, $\dfrac{T}{\frac{1}{2}\rho_w V_\infty{}^2 A_c}$

$C_{T,f}$ thrust coefficient based on frontal area, $\dfrac{F_T}{\frac{1}{2}\rho_w V_\infty{}^2 A_f}$

F_A axial force in forward direction, lb

$F_{A,o}$ axial force in forward direction with nose and tail fairings on model, lb

F_T net jet thrust, $F_A - F_{A,o}$, lb

M Mach number, V/a

m mass flow per unit time, slugs/sec

p static pressure, lb/sq ft

p_t total pressure, lb/sq ft

q dynamic pressure, $\frac{1}{2}\rho V^2$, lb/sq ft

R gas constant for air, $53.3 \dfrac{\text{ft-lb}}{\text{lb-}^\circ\text{F}}$

T internal thrust, lb

t water temperature, 505° R

V velocity, ft/sec

w weight flow, lb/sec

γ adiabatic exponent (assumed to be 1.4 for air)

$\bar{\gamma}$ average adiabatic exponent

η efficiency

η_{exp} experimental efficiency, $\dfrac{F_T V_\infty}{R t w_a \log \dfrac{p_d}{p_\infty}}$

ρ density, slugs/cu ft

ρ_w mass density of water, 1.970 slugs/cu ft

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Subscripts:

a air

c capture station

d diffuser exit

e exit station

f frontal

i inlet

m mixing chamber

w water

wa mixture of water and air

∞ free stream

I. EXPERIMENTAL INVESTIGATION OF HYDRODUCT

By Elmo J. Mottard

HYDRODUCT MODEL AND TEST APPARATUS

The hydroduct model and towing apparatus are shown in figures 1 and 2. The model was supplied by the manufacturer. The diffuser and nozzle sections were made of clear plastic to facilitate flow observation. These two pieces were screwed onto a brass midsection which was welded to a strut. The strut had a duct which connected a compressed-air supply to the interior of the brass midsection. Metered air was injected into the stream of water through a cylindrical screen with openings of 0.001 inch in diameter. The space between the screen and the outer shell of the midsection constituted an annular settling chamber.

Pressure-measurement orifices were provided at the maximum-diameter section of the diffuser and at three points around the inlet, located so that an axial asymmetry due to yaw, angle of attack, or distortion of the water surface could be detected. Provisions were made for keeping all lines to pressure taps completely filled with either air or water. Dynamometer measurements were made of the axial force on the model. Nose and tail fairings shaped as shown in figure 1 were used for tare measurements. The towing tank in which the tests were made is described in reference 3.

PROCEDURE

The speed and airflow rate were held constant during the tests. In order to minimize strut tares, the tests were made at a depth of only 1 inch to the top of the hydroduct body, with the exception of a few tests at a 7-inch depth to investigate surface effects. Speeds of 20, 30, 40, 50, and 60 feet per second and airflow rates of approximately 0.1, 0.2, and 0.3 pound per second were used. A reference drag of the strut and hydroduct was obtained with the nose and tail fairings in place.

RESULTS AND DISCUSSION

The axial-force coefficient is plotted as a function of the ratio of weight airflow to free-stream velocity in figure 3. It is evident

that the hydroduct is capable of producing thrust since positive values of the axial-force coefficient were obtained. The increase in draft from 1 inch to 7 inches produced a constant decrement in positive axial-force coefficient of 0.07, which is attributed to the increased strut drag. It is believed, therefore, that no surface effect, such as a reduction in ram pressure or change in flow at the exit due to proximity of the free surface, occurred.

The thrust coefficient, obtained by subtracting the drag coefficient of the body with the openings faired over from the axial-force coefficient, is presented in figure 4. It is apparent from figures 3 and 4 that the force coefficients are functions primarily of the ratio of weight airflow to free-stream velocity.

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The variation of the pressure coefficient at the inlet with the ratio of weight airflow to free-stream velocity is presented in figure 5. The variation of pressure around the inlet was small, an indication that the flow was nearly symmetrical radially despite the proximity of the free surface. The pressure coefficient at the diffuser exit is presented in figure 6. In a manner similar to the force coefficients, the pressure coefficients (figs. 5 and 6) are functions primarily of the ratio of weight airflow to free-stream velocity. The inlet velocity ratios were computed from the measured inlet pressures and are presented in figure 7. That the maximum inlet velocity ratio without airflow (1.53) approached the ratio of exit area to inlet area (1.65) indicates that the system total-pressure losses were small for these conditions. For free-stream velocities greater than 40 feet per second, cavitation occurred at the inner lip sections of the inlet which precluded determination of the correct inlet velocity ratio from inlet static-pressure measurements. The inlet velocity decreases with increasing airflow until air starts to "puff" out of the inlet; this occurs with a ratio of weight airflow to free-stream velocity greater than about 0.008. The inlet velocity can no longer be determined when air leaks out the inlet.

In figure 8 is presented the variation of propulsive efficiency with thrust coefficient. The propulsive efficiency is defined by

$$\text{Efficiency} = \frac{\text{Forward speed} \times \text{Net jet thrust}}{\text{Power available from expanding air}}$$

where the denominator is the power ideally available from an isothermal expansion of the existing airflow from the pressure measured in the diffuser to the free-stream pressure. An isothermal expansion was selected as a criterion approachable by the actual expansion because

of the great heat capacity of the water (relative to that of air) and the rapid heat-transfer rate attainable due to the mixing. Use of an adiabatic expansion as a criterion, for example, could result in efficiencies exceeding 100 percent. The maximum efficiency obtained in these tests was approximately 53 percent at a thrust coefficient of 0.10 and speed of 60 feet per second. The range of test variables, however, was insufficient to determine the maximum attainable efficiency as a function of thrust coefficient or free-stream velocity. The data indicated that the maximum efficiency probably occurs at a free-stream velocity greater than 60 feet per second (fig. 8). The hydroduct is comparable to a supercavitating propeller for application to a high-speed hydrofoil configuration; however, the thrust coefficient of 0.10 was small compared with that of 1.07 for a supercavitating propeller (computed from ref. 4).

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Consideration has been given to the possibility that exit choking has an effect on thrust production and efficiency. Examination of reference 5 indicated that air-water mixtures of the range of the investigation could have sonic velocities as low as 70 feet per second. In the absence of exit static-pressure measurements, exit velocities were calculated by assuming that the exit pressure was equal to the free-stream static pressure; most of the resultant values were equal to or greater than 70 feet per second, indicating the possibility of exit choking. If the exit flow was indeed choked, the exit pressures might be greater than the ambient pressure so that some additional expansion of the flow through the use of a larger exit area may produce greater thrust.

II. THEORETICAL INVESTIGATION OF AN AIR-POWERED UNDERWATER RAMJET

By Charles J. Shoemaker

ANALYSIS

This theoretical development is for an underwater ramjet that uses air as a "fuel." The ramjet cycle is as follows: the water captured by the inlet passes through a diffuser into a mixing chamber; here, compressed air is injected at the mixing-chamber pressure and water temperature; the resulting water-air mixture is discharged through an exit nozzle. Because the water-air mixture is less dense than pure water, the exit velocity exceeds the forward speed and a thrust results.

The procedure to be used in the theoretical development of an underwater ramjet cycle is as follows: A basic equation for thrust coefficient is presented and this basic equation is then evaluated by use of simplifying assumptions concerning the fluid properties. Two assumptions are made concerning the type of flow through the exit nozzle: (1) that the flow is incompressible and (2) that the flow is compressible.

Basic Thrust-Coefficient Equation

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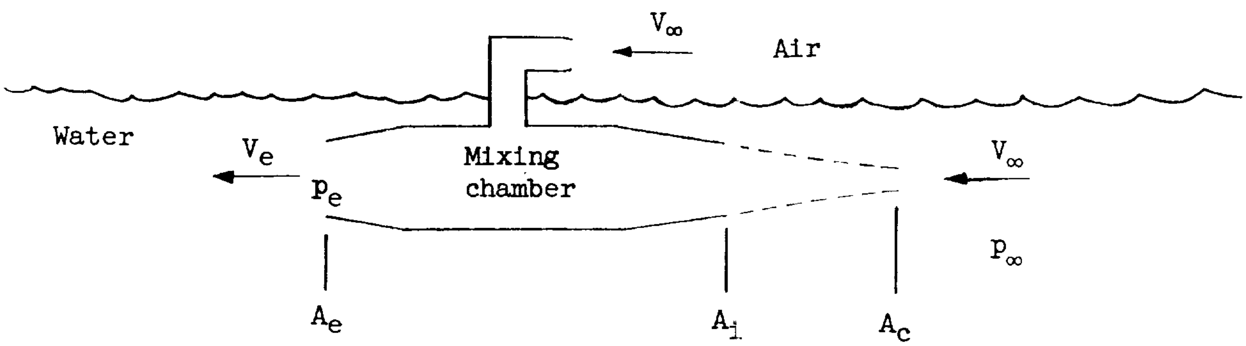
A simple underwater ramjet configuration is shown in sketch 1, as follows:



Sketch 1

The internal thrust of a ramjet cycle for steady flow is determined by the rate of change of momentum of the fluid passing through the ramjet. If the free-stream pressures of the air and water are assumed to be equal, then the internal thrust of the configuration shown in sketch 1 is

$$T = m_w\left(V_e - V_\infty\right) + m_a\left(V_e - V_\infty\right) + \left(p_e - p_\infty\right)A_e$$

where m_w and m_a are, respectively, the mass flow per unit time of water and of air entering the ramjet. By rearranging terms and using the equality $m_w = \rho_w V_\infty A_c$, the internal thrust becomes

$$T = 2\left(\frac{\rho_w V_\infty}{2}\right)^2 A_c\left(1 + \frac{m_a}{m_w}\right)\left(\frac{V_e}{V_\infty} - 1\right) + \left(p_e - p_\infty\right)A_c$$

The thrust coefficient based on capture area is defined as

$$C_{T,c} = \frac{T}{\frac{1}{2} \rho_w V_\infty^2 A_c}$$

Upon substituting the thrust relation into the preceding equation and canceling terms, the following basic equation for internal thrust coefficient is obtained:

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$$C_{T,c} = 2\left(1 + \frac{m_a}{m_w}\right)\left(\frac{V_e}{V_\infty} - 1\right) + \frac{(p_e - p_\infty)A_e}{\frac{1}{2} \rho_w V_\infty^2 A_c} \tag{1}$$

Subsonic Discharge

A basic assumption of this analysis is that the ramjet operates with a subsonic exit. For a subsonic exit, it can be demonstrated that the exit and free-stream pressures are equal - that is, $p_e = p_\infty$. Then examination of equation (1) shows that the term containing the pressure and areas becomes zero.

To evaluate the thrust coefficient requires the determination of the velocity ratio V_e/V_∞. This ratio is most easily evaluated by studying, in succession, the processes of induction, mixing, and discharge. The assumptions for the induction and mixing processes remain the same regardless of whether the exit nozzle flow is treated as incompressible or compressible.

Inlet and diffuser.- In the induction process it is assumed that the water acts as an incompressible inviscid fluid. Also, the diffuser is assumed to deliver the water to the mixing chamber at a negligible velocity. With the use of these assumptions and Bernoulli's equation, the total pressure in the mixing chamber can be expressed as

$$p_{t,m} = p_\infty + \frac{1}{2} \rho_w V_\infty^2 \tag{2}$$

Mixing of air with water.- The assumptions of the mixing process are: the air is injected at the total pressure and total temperature of the water, the air and water mix homogeneously without loss in total

pressure, the air does not dissolve in the water nor does the water form a vapor, and the air and water components always have the same velocity.

<u>Incompressible flow in exit nozzle</u>.- One method of analyzing the flow through the exit nozzle is to treat the mixture of air and water as an incompressible fluid. The nozzle total pressure is assumed to be a constant and to be equal to the mixing-chamber pressure.

Velocity in relation to density: Bernoulli's equation for incompressible flow evaluated at the nozzle exit is

$$p_{t,m} = p_e + \frac{1}{2} \rho_{wa,e} V_e^2$$

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Substituting the mixing-chamber total pressure (eq. (2)) into this equation and using the relation $p_\infty = p_e$ results in

$$p_\infty + \frac{1}{2} \rho_w V_\infty^2 = p_\infty + \frac{1}{2} \rho_{wa,e} V_e^2$$

Solving now for the velocity ratio V_e/V_∞ gives

$$\frac{V_e}{V_\infty} = \sqrt{\frac{\rho_w}{\rho_{wa,e}}} \tag{3}$$

Density ratio: The following development gives a general expression for the density ratio. The volume flow of the water-air mixture at a given station is

$$\text{Volume flow} = \frac{m_{wa}}{\rho_{wa}} = \frac{(m_w + m_a)}{\rho_{wa}} = \left(\frac{m_w}{\rho_w} + \frac{m_a}{\rho_a}\right)$$

Solving for $1/\rho_{wa}$ gives

$$\frac{1}{\rho_{wa}} = \frac{\left(\frac{m_w}{\rho_w} + \frac{m_a}{\rho_a}\right)}{\left(m_w + m_a\right)} = \frac{\frac{1}{m_w}\left(\frac{1}{\rho_w} + \frac{m_a}{m_w}\frac{1}{\rho_a}\right)}{\frac{1}{m_w}\left(1 + \frac{m_a}{m_w}\right)}$$

If both sides of the equation are multiplied by ρ_w and then simplified, the following general expression is obtained for the density ratio:

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$$\frac{\rho_w}{\rho_{wa}} = \frac{\left(1 + \frac{m_a}{m_w}\frac{\rho_w}{\rho_a}\right)}{\left(1 + \frac{m_a}{m_w}\right)} \tag{4}$$

Note that the air and the mixture densities are associated values and must be evaluated at a common point.

Velocity ratio: The complete expression for the velocity ratio in an incompressible mixture of air and water is obtained from equations (3) and (4) and is

$$\frac{V_e}{V_\infty} = \sqrt{\frac{\left(1 + \frac{m_a}{m_w}\frac{\rho_w}{\rho_{a,e}}\right)}{\left(1 + \frac{m_a}{m_w}\right)}} \tag{5}$$

Compressible flow in exit nozzle.- Another method of analyzing the flow through the exit nozzle is to treat the water-air mixture as a compressible fluid. This method is preferred because the actual mixture is compressible.

For this development, the nozzle total pressure is again assumed to be a constant and equal to the mixing-chamber pressure and the mixture of air and water is assumed to act as a compressible gas. By this approach, the velocity ratio is obtained as a function of an appropriate adiabatic exponent γ. The estimation of γ is discussed in detail in a subsequent section.

Velocity in relation to density: In order to obtain the velocity ratio V_e/V_∞, the following compressible-flow relation is used:

$$\frac{p_{t,wa}}{p_\infty} = \left(1 + \frac{\gamma - 1}{2} M_e^2\right)^{\frac{\gamma}{\gamma-1}}$$

Subtracting 1 from each side of this equation and inserting $M_e^2 = V_e^2 / a_e^2 = V_e^2 \Big/ \left(\gamma p_e / \rho_{wa,e}\right)$ gives

$$\left(\frac{p_{t,wa}}{p_\infty} - 1\right) = \left[\left(1 + \frac{\gamma - 1}{2\gamma} \frac{\rho_{wa,e} V_e^2}{p_e}\right)^{\frac{\gamma}{\gamma-1}} - 1\right]$$

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with the assumption that $p_{t,m} = p_{t,wa}$, the mixing-chamber total pressure obtained from equation (2) can be rearranged to give $\frac{q_\infty}{p_\infty} = \frac{p_{t,wa}}{p_\infty} - 1$. Using this relation in the preceding equation and solving for V_e produces

$$V_e = \sqrt{\frac{p_e}{\frac{1}{2} \rho_{wa,e}} \frac{\gamma}{\gamma - 1} \left[\left(\frac{q_\infty}{p_\infty} + 1\right)^{\frac{\gamma-1}{\gamma}} - 1\right]}$$

Dividing both sides of this equation by V_∞ and inserting ρ_w / ρ_w under the radical gives

$$\frac{V_e}{V_\infty} = \sqrt{\frac{p_e \frac{\gamma}{\gamma - 1}}{\frac{1}{2} \frac{\rho_{wa,e}}{\rho_w} \rho_w V_\infty^2} \left[\left(\frac{q_\infty}{p_\infty} + 1\right)^{\frac{\gamma-1}{\gamma}} - 1\right]}$$

When $p_e = p_\infty$ is substituted and the identity $q_\infty = \frac{1}{2} \rho_w V_\infty^2$ is used in the preceding equation, rearranging the resulting equation produces the following expression for the velocity ratio:

$$\frac{V_e}{V_\infty} = \sqrt{\left(\frac{\rho_w}{\rho_{wa,e}}\right)\left\{\frac{\frac{\gamma}{\gamma - 1}\left[\left(\frac{q_\infty}{p_\infty} + 1\right)^{\frac{\gamma-1}{\gamma}} - 1\right]}{q_\infty/p_\infty}\right\}} \tag{6}$$

This velocity ratio (eq. (6)) is the incompressible-flow relation (eq. (3)) multiplied by a function to compensate for compressibility. The term under the radical which is enclosed by the braces is referred to herein as a compressibility function and is designated $f(\gamma)$ because it contains an unknown γ.

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Velocity ratio: The substitution of equation (4) into equation (6) gives the complete velocity ratio for compressible flow, which is

$$\frac{V_e}{V_\infty} = \sqrt{\frac{\left(1 + \frac{m_a}{m_w}\frac{\rho_w}{\rho_{a,e}}\right)}{\left(1 + \frac{m_a}{m_w}\right)}\left\{\frac{\frac{\gamma}{\gamma - 1}\left[\left(\frac{q_\infty}{p_\infty} + 1\right)^{\frac{\gamma-1}{\gamma}} - 1\right]}{q_\infty/p_\infty}\right\}} \tag{7}$$

Estimation of adiabatic exponent: To obtain the adiabatic exponent γ needed in equation (7), the following relation is used:

$$\frac{d\left(\log_e p\right)}{d\left(\log_e \rho\right)} = \gamma \tag{8}$$

Equation (8) can be obtained from the speed-of-sound equation or from the relation $p = \text{Constant} \times \rho^\gamma$. The latter relation is used in this development. Differentiation of this relation yields

$$dp = \text{Constant} \times \gamma\left(\frac{\rho^\gamma}{\rho}\right)d\rho$$

Substituting p for the quantity $\text{Constant} \times \rho^\gamma$ and rearranging gives

$$\frac{dp}{p} = \gamma\,\frac{d\rho}{\rho}$$

Using the relations $d(\log_e p) = \frac{dp}{p}$ and $d(\log_e \rho) = \frac{d\rho}{\rho}$ produces

$$\frac{d(\log_e p)}{d(\log_e \rho)} = \gamma$$

The evaluation of γ is begun by differentiating equation (4) with ρ_{wa} and ρ_a as the variables. When equation (4) is inverted and differentiated, the result is

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$$\frac{d\rho_{wa}}{\rho_w} = \frac{\left(1 + \frac{m_a}{m_w}\right)\left(\frac{m_a \rho_w \, d\rho_a}{m_w \rho_a^2}\right)}{\left(1 + \frac{m_a}{m_w}\frac{\rho_w}{\rho_a}\right)^2}$$

Multiply both sides by ρ_w/ρ_{wa} to obtain

$$\frac{d\rho_{wa}}{\rho_{wa}} = \frac{\frac{\rho_w}{\rho_{wa}}\left(1 + \frac{m_a}{m_w}\right)\left(\frac{m_a}{m_w}\right)\left(\frac{\rho_w}{\rho_a}\right)\left(\frac{d\rho_a}{\rho_a}\right)}{\left(1 + \frac{m_a}{m_w}\frac{\rho_w}{\rho_a}\right)^2}$$

Replacing ρ_w/ρ_{wa} by equation (4) and canceling like terms gives

$$\frac{d\rho_{wa}}{\rho_{wa}} = \left(\frac{\frac{m_a}{m_w}\frac{\rho_w}{\rho_a}}{1 + \frac{m_a}{m_w}\frac{\rho_w}{\rho_a}}\right)\left(\frac{d\rho_a}{\rho_a}\right)$$

However, $\frac{d\rho}{\rho} = d(\log_e \rho)$ and therefore

$$d\left(\log_e \rho_{wa}\right) = d\left(\log_e \rho_a\right)\left(\frac{\frac{m_a}{m_w}\frac{\rho_w}{\rho_a}}{1 + \frac{m_a}{m_w}\frac{\rho_w}{\rho_a}}\right)$$

Invert this relation and multiply by $d(\log_e p)$ to obtain

$$\frac{d(\log_e p)}{d\left(\log_e \rho_{wa}\right)} = \frac{d(\log_e p)}{d\left(\log_e \rho_a\right)} = \left(\frac{1 + \frac{m_a}{m_w}\frac{\rho_w}{\rho_a}}{\frac{m_a}{m_w}\frac{\rho_w}{\rho_a}}\right)$$

By use of equation (8) and rearranging terms, the following general relation for γ is found:

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$$\gamma = \gamma_a\left(1 + \frac{m_w}{m_a}\frac{\rho_a}{\rho_w}\right) \tag{9}$$

As the mixture travels through the nozzle, γ is continually changing. Therefore, the γ used to evaluate equation (7) was an average of the mixing-chamber and exit values. The average value of γ is

$$\bar{\gamma} = \frac{\gamma_{wa,e} + \gamma_m}{2}$$

Substituting for $\gamma_{wa,e}$ and γ_m by using equation (9) and rearranging terms produces

$$\bar{\gamma} = \gamma_a\left[1 + \frac{1}{2}\frac{m_w}{m_a}\frac{\rho_{a,e}}{\rho_w}\left(1 + \frac{\rho_{a,m}}{\rho_{a,e}}\right)\right]$$

If it is assumed that the air expands isothermally, the relation $\rho = p/RT$ shows that ρ is proportional to p. Then, noting that $p_m = p_t = p_\infty + q_\infty$ yields

$$\bar{\gamma} = \gamma_a\left[1 + \frac{1}{2}\frac{m_w}{m_a}\frac{\rho_{a,e}}{\rho_w}\left(1 + \frac{p_\infty + q_\infty}{p_e}\right)\right]$$

For the assumption that the nozzle is not choked, the free-stream pressure equals the exit pressure ($p_\infty = p_e$). Making this substitution in the preceding equation and combining terms gives the expression for the average adiabatic exponent:

$$\bar{\gamma} = \gamma_a \left[1 + \frac{m_w}{m_a} \frac{\rho_{a,e}}{\rho_w}\left(1 + \frac{1}{2} \frac{q_\infty}{p_\infty}\right)\right] \tag{10}$$

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In the differentiation leading to equations (9) and (10), ρ_w was assumed to be constant. This is a reasonable assumption for the thrust calculations. However, if it is desired to have an expression for the speed of sound which covers the range from air only to water only, then the compressibility of water must be included.

Component efficiencies.- Throughout this theoretical development, the maximum thrust coefficient was obtained by assuming that the component losses were negligible. In this section, a procedure for including the effect of the various component losses is given. When the analytical and measured thrust coefficients are compared, the losses can be estimated.

The effect of the component losses can be expressed as efficiencies in the following manner. Let the inlet efficiency η_i be defined so that the mixing-chamber total pressure is

$$p_{t,m} = p_\infty + \eta_i\left(\frac{1}{2} \rho_w V_\alpha^{\,2}\right)$$

Using this inlet total pressure and solving for the velocity ratio, by the method used in the section entitled "Incompressible flow in exit nozzle," gives a relation similar to equation (3). The velocity ratio is now

$$\frac{V_e}{V_\infty} = \sqrt{\eta_i\left(\frac{\rho_w}{\rho_{wa,e}}\right)}$$

The combined efficiencies of the inlet, mixing process, and exit nozzle are defined as an overall efficiency $g(\eta)$. With $g(\eta)$, the incompressible velocity ratio is then

$$\frac{V_e}{V_\infty} = \sqrt{g(\eta)\left(\frac{\rho_w}{\rho_{wa,e}}\right)}$$

By analogy, equation (6) which is for compressible nozzle flow becomes

$$\frac{V_e}{V_\infty} = \sqrt{g(\eta)\left(\frac{\rho_w}{\rho_{wa,e}}\right)f(\gamma)} \tag{11}$$

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Working Form of Thrust-Coefficient Equation

The assumptions made to obtain the working form of the thrust-coefficient equation which was used to study the experimental data are again stated.

Throughout the ramjet cycle, the component losses were neglected. The water was considered to be an incompressible fluid. In the mixing chamber the water velocity was negligible. The injection of air resulted in a uniform mixture that did not separate. This mixture was discharged through a nozzle with a subsonic velocity. The nozzle total pressure was constant and equal to the mixing-chamber pressure. Static pressure at the nozzle exit was equal to the free-stream static pressure. The mixture behaved as a compressible gas. An evaluation of the average synthetic γ, based on an isothermal expansion, was made for this gas.

The internal thrust coefficient for subsonic compressible nozzle flow obtained from equations (1) and (7) is

$$C_{T,c} = 2\left(1 + \frac{m_a}{m_w}\right)\left(\sqrt{\frac{\left(1 + \frac{m_a}{m_w}\frac{\rho_w}{\rho_{a,e}}\right)}{\left(1 + \frac{m_a}{m_w}\right)}\left\{\frac{\frac{\gamma}{\gamma - 1}\left[\left(\frac{q_\infty}{p_\infty} + 1\right)^{\frac{\gamma-1}{\gamma}} - 1\right]}{q_\infty/p_\infty}\right\}} - 1\right) \tag{12}$$

An average value of γ, given by equation (10), was used.

Sonic Discharge Considerations

As mentioned previously for subsonic discharge, the nozzle exit pressure and free-stream pressure are equal. If the exit velocity is equal to sonic velocity, the exit and free-stream pressures are not necessarily equal and a different procedure is necessary to evaluate the thrust coefficient. Herein are given two ways of estimating the sonic velocity of a water-air mixture. Then, a suggested procedure to determine the thrust for a sonic discharge is presented.

Estimation of sonic velocity.- The speed of sound of the mixture is a function of the component densities which are determined by their respective temperatures. There are two expansion processes which bracket the temperature range. One is an isentropic expansion and the second is an isothermal expansion. The speed of sound obtained from each of these expansions is presented and compared.

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Isentropic expansion: If the water-air mixture is assumed to pass through the nozzle so quickly that there is little time for heat transfer, the expansion process is essentially adiabatic. Also, the losses in a nozzle are assumed to be negligible since they are usually small. With these assumptions, the expansion process is isentropic.

A relation for the speed of sound is obtained by use of the following equation for the velocity of sound:

$$\frac{1}{a^2} = \frac{\partial \rho}{\partial p} \tag{13}$$

The right-hand side of equation (13) is evaluated by the use of the mixture density obtained from equation (4) which is

$$\rho_{wa} = \rho_w\left(1 + \frac{m_a}{m_w}\right)\left(1 + \frac{m_a}{m_w}\frac{\rho_w}{\rho_a}\right)^{-1}$$

Here the water is to be treated as a compressible fluid; then ρ_w, ρ_{wa}, and ρ_a are associated variables and must be evaluated at a common point. When both sides of the equation are divided by a common pressure and differentiated with respect to this pressure, the following relation is obtained by rearranging terms:

$$\frac{d\rho_{wa}}{dp} = \left(1 + \frac{m_a}{m_w}\right)\left[\frac{\left(\frac{\rho_a}{\rho_w}\right)^2 \frac{\partial\rho_w}{\partial p} + \frac{m_a}{m_w}\frac{\partial\rho_a}{\partial p}}{\left(\frac{\rho_a}{\rho_w} + \frac{m_a}{m_w}\right)^2}\right]$$

Using equation (13) in the preceding equation yields

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$$\frac{1}{a_{wa}{}^2} = \left(1 + \frac{m_a}{m_w}\right)\left[\frac{\left(\frac{\rho_a}{\rho_w}\right)^2 \frac{1}{a_w{}^2} + \frac{m_a}{m_w}\frac{1}{a_a{}^2}}{\left(\frac{\rho_a}{\rho_w} + \frac{m_a}{m_w}\right)^2}\right]$$

Solving for a_{wa} produces the following general relation for the speed of sound in the mixture:

$$a_{wa} = a_a\left\{\frac{\left[1 + \left(\frac{m_w}{m_a}\right)\left(\frac{\rho_a}{\rho_w}\right)\right]\sqrt{\frac{m_a/m_w}{1 + m_a/m_w}}}{\sqrt{1 + \left(\frac{m_w}{m_a}\right)\left(\frac{\rho_a}{\rho_w}\right)^2\left(\frac{a_a}{a_w}\right)^2}}\right\} \tag{14}$$

For mass-flow ratios of the data which were sufficient to produce thrust, the denominator of equation (14) is essentially unity.

If the water had been assumed to be incompressible, the speed-of-sound equation would be

$$a_{wa} = a_a\sqrt{\frac{m_a}{m_w + m_a}}\left[1 + \left(\frac{m_w}{m_a}\right)\left(\frac{\rho_a}{\rho_w}\right)\right] \tag{15}$$

Equation (15) can be reduced to the numerator of equation (14).

Isothermal expansion: Another method to obtain the speed-of-sound equation is given by Prandtl in reference 5. Prandtl assumed the air temperature to be equal to the practically constant water temperature.

The water density was assumed to be constant. Also, the total mass flow was chosen to be unity. The speed-of-sound relation given by Prandtl is equivalent to

$$a_{wa} = \sqrt{\frac{m_a p}{\rho_a}\left[1 + \left(\frac{m_w}{m_a}\right)\left(\frac{\rho_a}{\rho_w}\right)\right]} \tag{16}$$

Comparison of speed-of-sound relations: In order to compare the L
two speed-of-sound relations, equation (15) was modified. Substituting 1
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$a_a = \sqrt{\gamma_a(p/\rho_a)}$ and $m_w + m_a = 1$ into equation (15) and rearranging 4
gives 9

$$a_{wa} = \sqrt{\gamma_a}\sqrt{\frac{m_a p}{\rho_a}\left[1 + \left(\frac{m_w}{m_a}\right)\left(\frac{\rho_a}{\rho_w}\right)\right]} \tag{17}$$

When water is assumed to be incompressible, the sonic velocity of a water-air mixture for an isothermal expansion (eq. (16)) is less than the sonic velocity for an isentropic expansion (eq. (17)) by the factor $\sqrt{\gamma_a}$.

Choked flow.- When sonic velocity is obtained at the exit of a converging nozzle, there are two distinguishing features about the flow: First, the pressure at the exit is generally unequal to the free-stream pressure and, second, the nozzle is discharging its maximum mass flow (choked flow). This choking of the exit restricts the total mass passing through the ramjet.

In the analysis of a ramjet cycle with choked exit flow, the assumptions for the inlet, diffusion, and mixing processes are the same as those for nonchoked exit flow. The mixing-chamber velocity is again assumed to be negligible. If the mixture of water and air is assumed to behave as a compressible gas, the mixture properties can be evaluated as a function of an adiabatic exponent γ. By assuming an exit pressure p_e that is equal to or greater than p_∞, γ can be obtained from equation (9). For an exit Mach number of unity and with γ, the other variables in equation (1) can be calculated by using the compressible gas relation for p_t/p, Bernoulli's equation, and the continuity equation.

In a comparison of the nozzle exit velocity (eq. (7)) and the sonic velocity of the water-air mixture (eq. (14)), it was found that all of the forward-velocity data at 50 and 60 feet per second had reached or slightly exceeded sonic velocity at the nozzle exit. Inasmuch as the theoretical thrust coefficients computed by equation (12) are not for choked flow, a separate analysis is required for these data. However, an exploratory computation for a forward velocity of 60 feet per second and a mass-flow ratio of 0.02 indicates that the thrust coefficient for choked flow is only about 3 percent more than that for the subsonic flow.

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Determination of Thrust Coefficient

The computations for the theoretical thrust coefficient are based on compressible nozzle flow with a subsonic exit velocity.

Compressibility function.- In order to evaluate the compressibility function $f(\gamma)$ used in equation (6), the density of the air at the exit is needed. This density was obtained by assuming that the air temperature was equal to the free-stream water temperature of 505° R.

Figure 9 is a plot of $f(\gamma)$ as a function of q_{∞}/p_{∞} for several values of m_a/m_w. The compressibility functions calculated for the particular values of m_a/m_w obtained from the data of part I are included in the figure.

Theoretical internal thrust coefficient.- Because the experimental thrust coefficients are based on frontal area, the theoretical thrust coefficients have been based on frontal area by multiplying equation (12) by the area ratios (A_c/A_e) and (A_e/A_f). The first area ratio is obtained from the continuity equation and is

$$\frac{A_c}{A_e} = \left[\frac{V_e/V_{\infty}}{1 + \left(\frac{m_a}{m_w}\right)\left(\frac{\rho_w}{\rho_a}\right)}\right]$$

The second area ratio A_e/A_f is obtained from the model dimensions. Figure 10 shows the relation between the internal thrust coefficient based on frontal area and the mass-flow ratio m_a/m_w for several values of $f(\gamma)$. The thrust coefficients evaluated for the particular mass-flow ratios obtained from the data of part I are plotted in this figure, with the values of $f(\gamma)$ indicated beside them.

Figure 9 can be used in conjunction with figure 10 to provide an estimation of the theoretical thrust coefficient. By choosing a forward velocity of the ramjet, the ratio q_∞/p_∞ can be calculated. Then, for an assumed mass-flow ratio m_a/m_w, figure 9 gives the theoretical compressibility function $f(\gamma)$. With $f(\gamma)$ and the assumed m_a/m_w, from figure 10 the corresponding theoretical thrust coefficient can be estimated.

COMPARISON OF THEORY WITH EXPERIMENT

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Thrust Coefficient

The theoretical and measured thrust coefficients are presented in figure 11. The theoretical values have been computed by using the mass flow obtained from the data of part I and have been faired according to values of constant forward velocity. The differences in the measured and theoretical thrust coefficients are due to three factors: (1) The experimental method of obtaining the measured thrust coefficient does not give the exact internal thrust coefficient. (2) There are internal losses associated with the inlet, the mixing process, and the exhaust nozzle. (3) The theory depends on the validity of the assumptions used to describe the mixture properties. With the viewpoint that the effect of factor (1) is negligible and the assumptions of factor (3) are valid, the internal losses become responsible for the differences in the theoretical and measured thrust coefficients.

Efficiency

When the efficiencies of the ramjet components are taken into consideration, the curves of figure 10 represent the product $\left[f(\gamma)\right]\left[g(\eta)\right]$, as indicated by equation (11). The value of this product is obtained by transferring the data points of figure 11 to figure 10. It is seen that most of the data lie close to a value of $\left[f(\gamma)\right]\left[g(\eta)\right] = 0.6$. An estimation of the overall efficiency is obtained by dividing the previous relation by the $\left[f(\gamma)\right]$ of the data which is given in figure 10. Then, by using an attainable inlet efficiency, the mixing and exit nozzle efficiencies can be estimated.

In the estimation of the inlet efficiency, it is assumed there is isentropic compression between the capture and inlet stations. For the remaining compression, regular subsonic diffuser losses are used. From subsonic diffuser data, these regular subsonic diffuser losses are known to depend on the diffuser wall angle and to be a certain percent of the losses for a sudden expansion to the same area ratio. For the inlet configuration used in the experiment, the efficiency is estimated to be

$$\eta_i = \left[1 - 0.1\left(\frac{V_i}{V_\infty}\right)^2\right]$$

The solid curves of figure 12 give the overall efficiency as a function of m_a/m_w. The dashed curves in figure 12 are the results of dividing the overall efficiency by the estimated inlet efficiency; this gives an estimation of the mixing and nozzle efficiency. Inasmuch as the nozzle efficiency is usually very close to unity, these dashed curves could be assumed to be the mixing efficiency.

CONCLUDING REMARKS

It has been found from a preliminary experimental investigation of an underwater ramjet that a positive thrust-minus-drag force was produced at every test speed. The force and pressure coefficients were functions primarily of the ratio of weight airflow to free-stream velocity for the conditions of the investigation. The maximum propulsive efficiency based on the net internal thrust and an isothermal expansion of the air was approximately 53 percent at a thrust coefficient of 0.10. The performance of the test model may have been influenced by choking of the exit flow. A more complete study is required to determine the applicability of the hydroduct.

The basic assumption of the theoretical analysis is that a mixture of air and water can be treated as a compressible gas. However, more information on the properties of air-water mixtures is required to confirm this assumption or to suggest another approach. A method is suggested from which a more complete theoretical development, with the

effects of choking included, may be obtained. An exploratory computation, in which this suggested method was used, indicated that the effect of choked flow on the thrust coefficient was minor.

Langley Research Center,
National Aeronautics and Space Administration,
Langley Air Force Base, Va., September 28, 1961.

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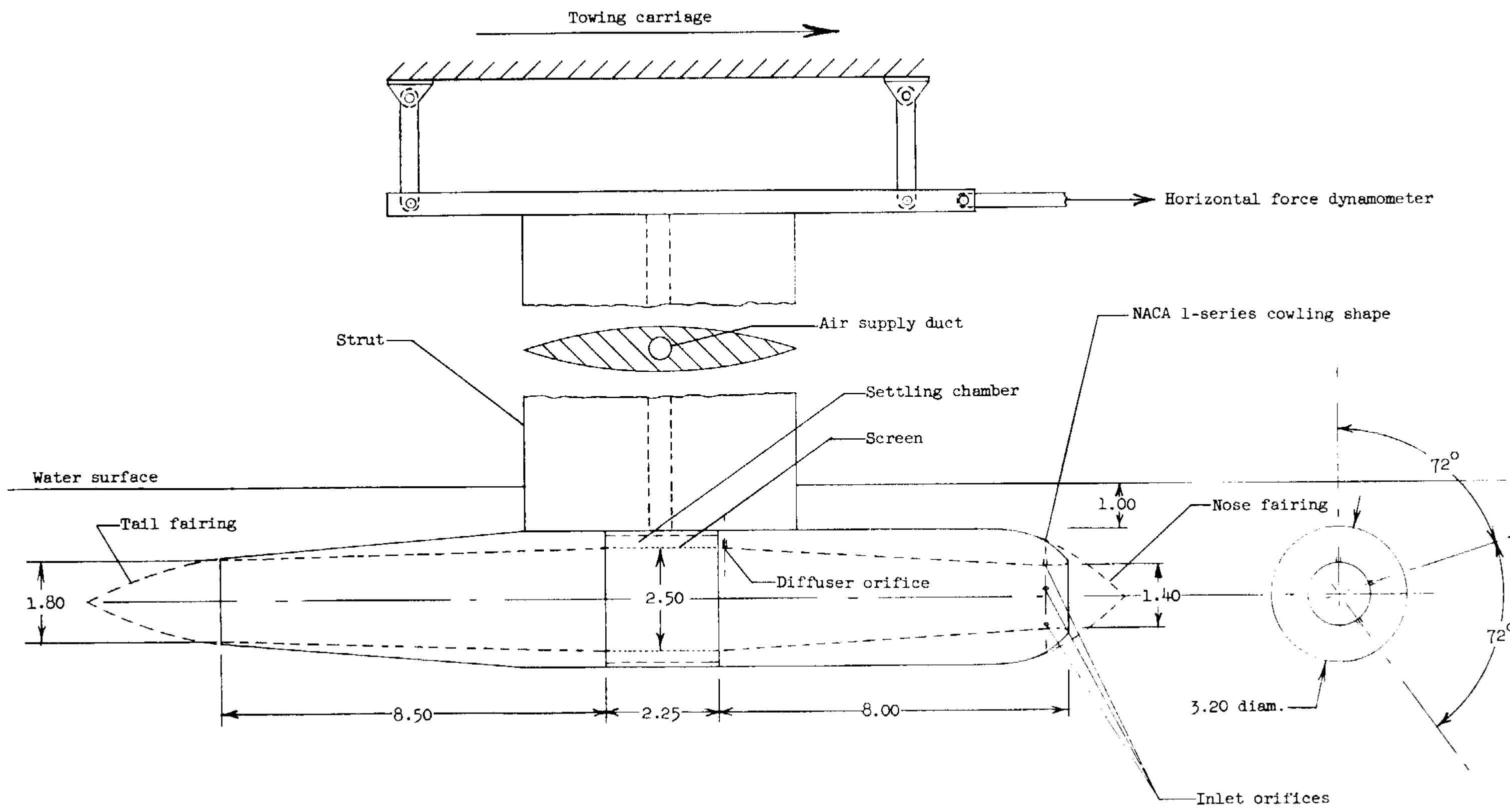

Figure 1.- Schematic sketch of hydroduct and test setup. Linear dimensions are in inches.

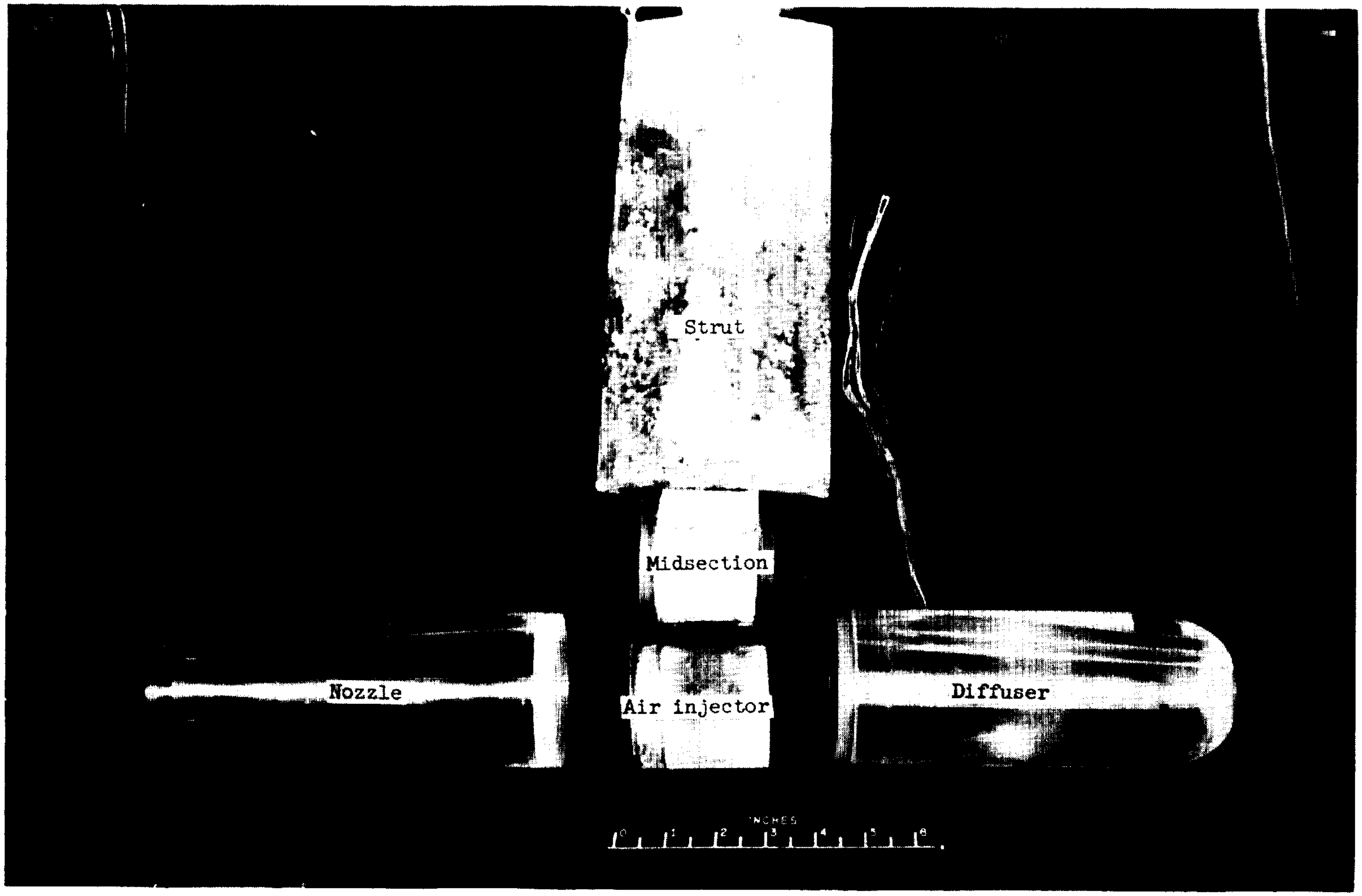

Figure 2.- Photograph of hydroduct model. L-60-1921.1

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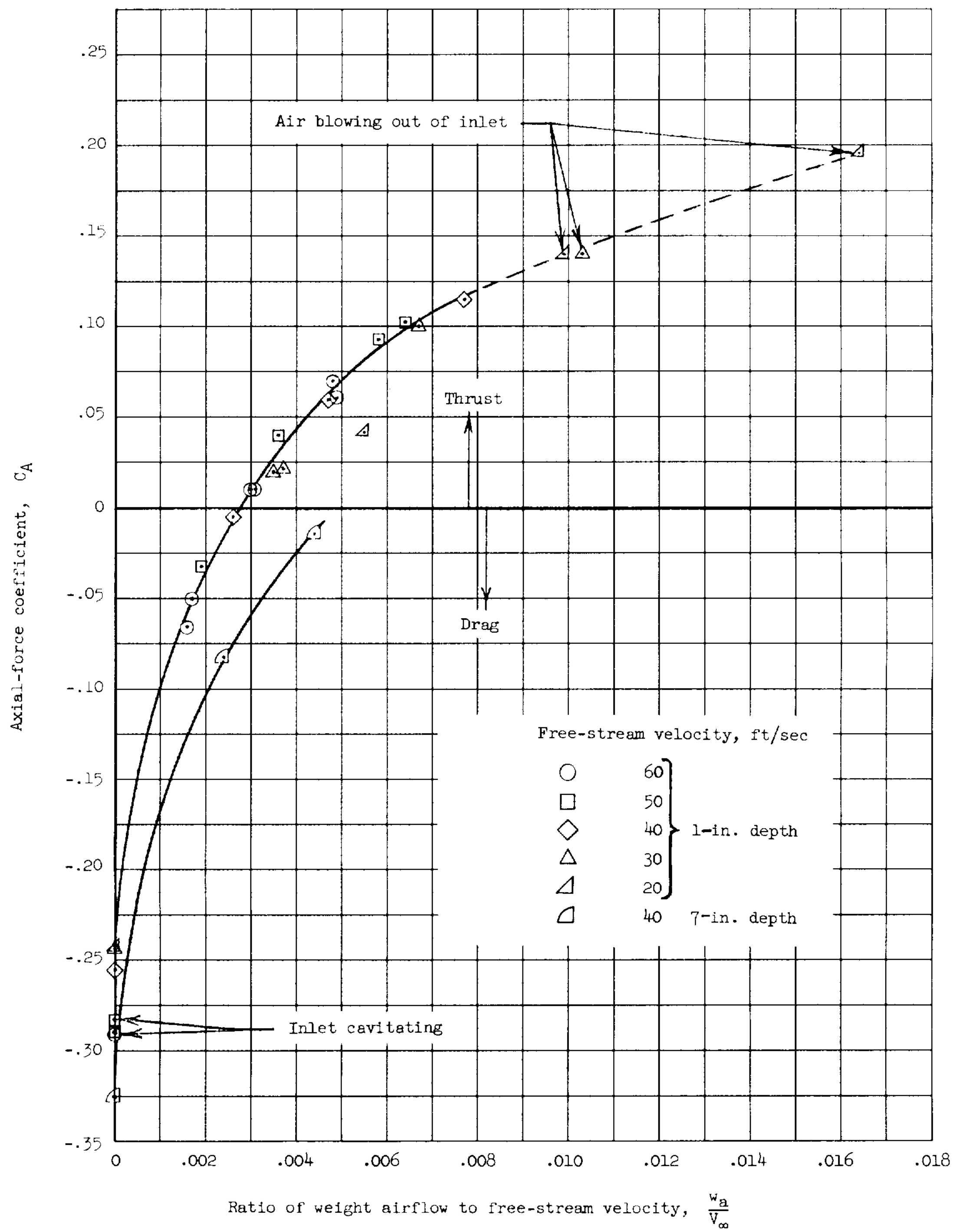

Figure 3.- Variation of the axial-force coefficient with the ratio of the weight airflow to the free-stream velocity.

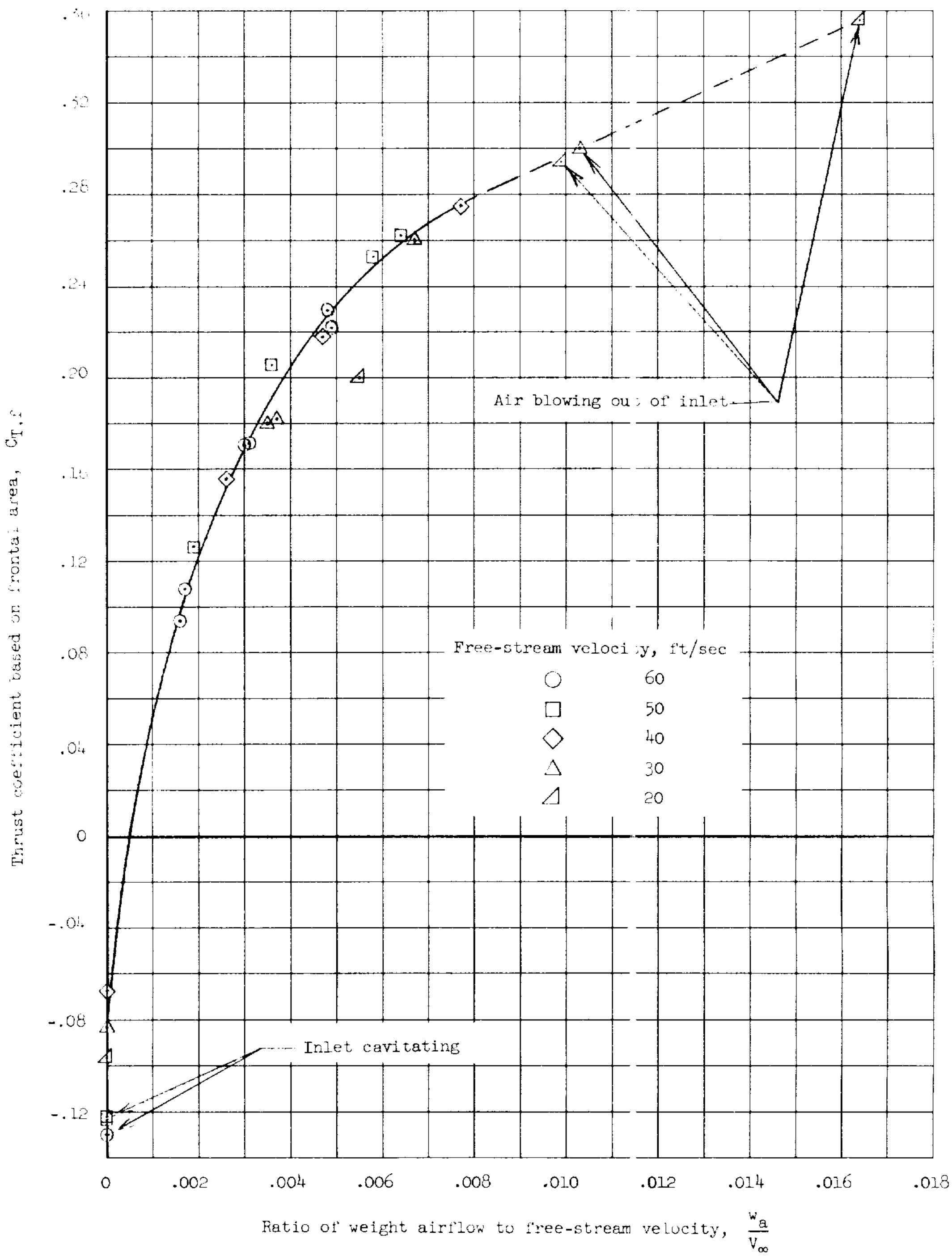

Figure 4.- Variation of the thrust coefficient based on frontal area with the ratio of weight airflow to free-stream velocity.

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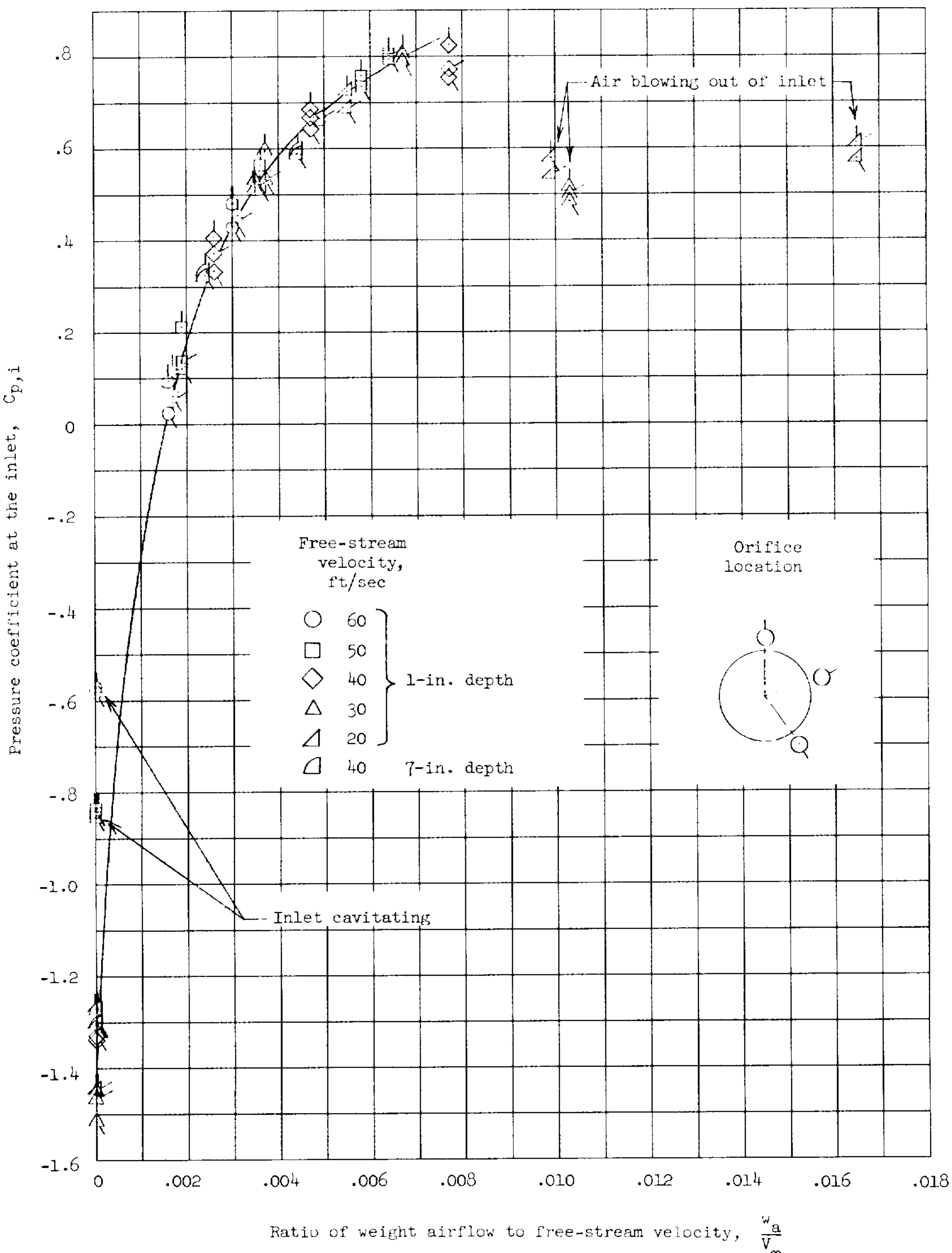

Figure 5.- Variation of the pressure coefficient at the inlet with the ratio of weight airflow to free-stream velocity.

Figure 7.- Variation of the inlet velocity ratio with the ratio of weight airflow to free-stream velocity.

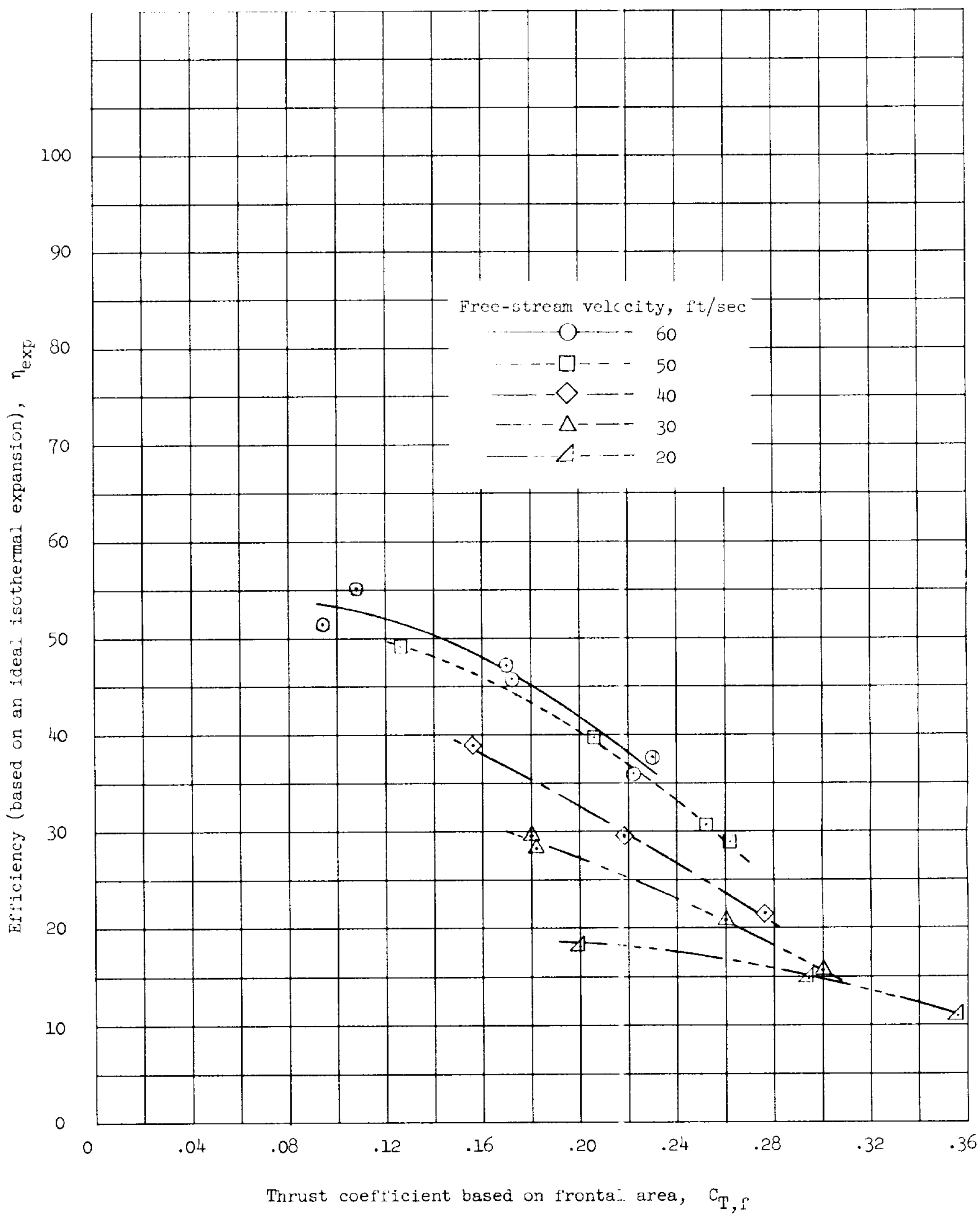

Figure 8.- Variation of efficiency with thrust coefficient.

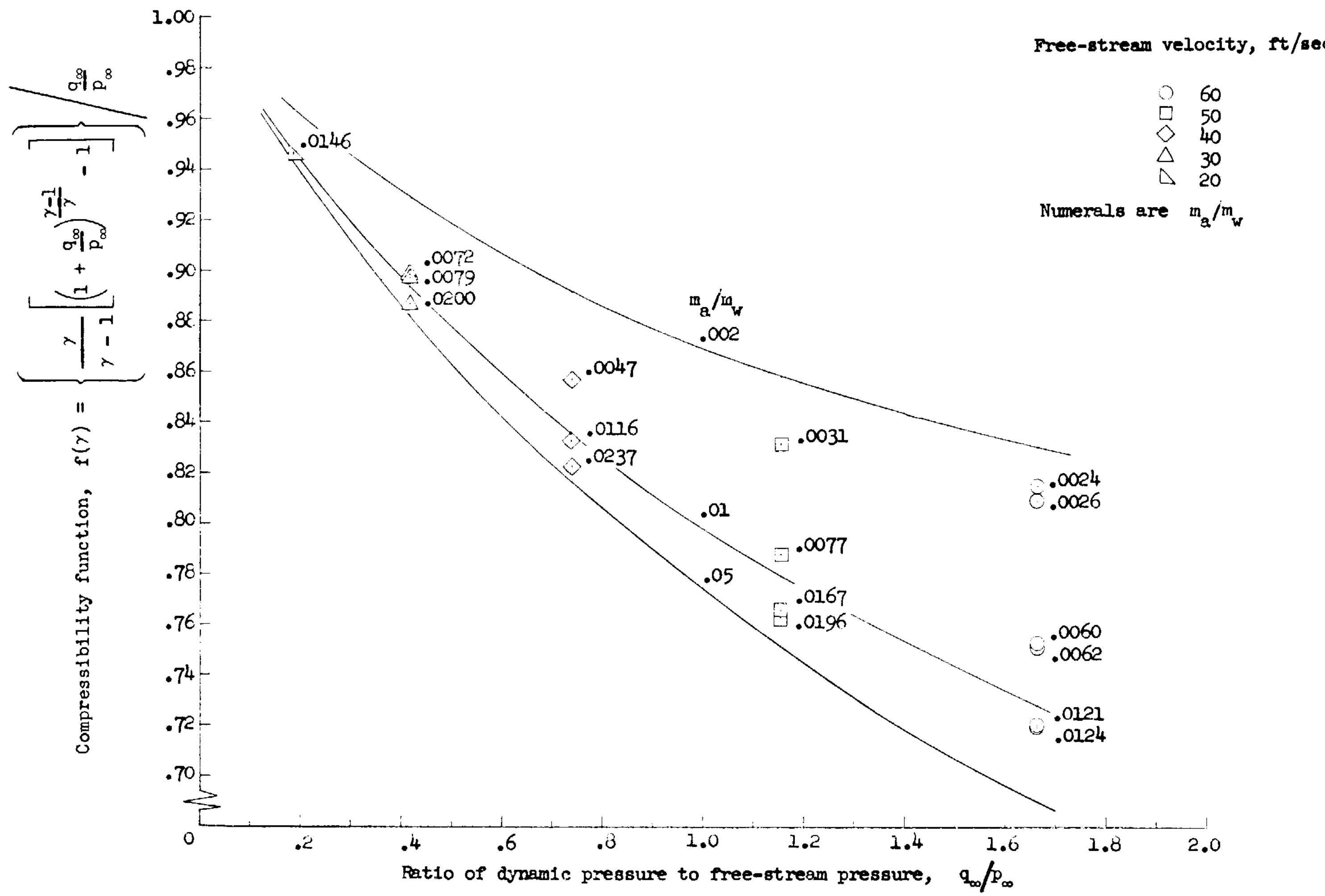

Figure 9.- Compressibility function. m_a based on mixing-chamber pressure and 505° R.

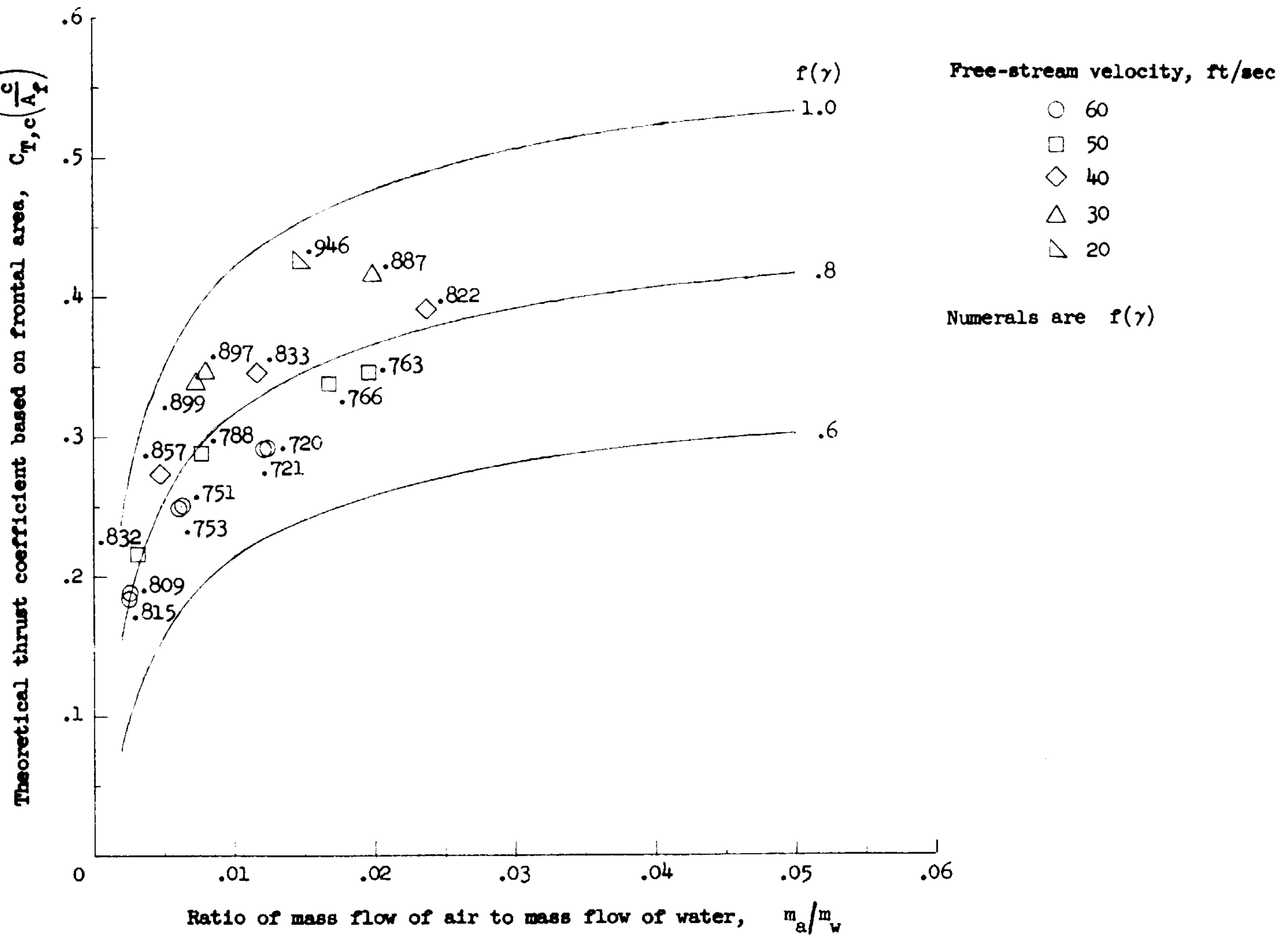

Figure 10.- Theoretical thrust coefficient based on frontal area. m_a based on mixing-chamber static pressure and 505° R.

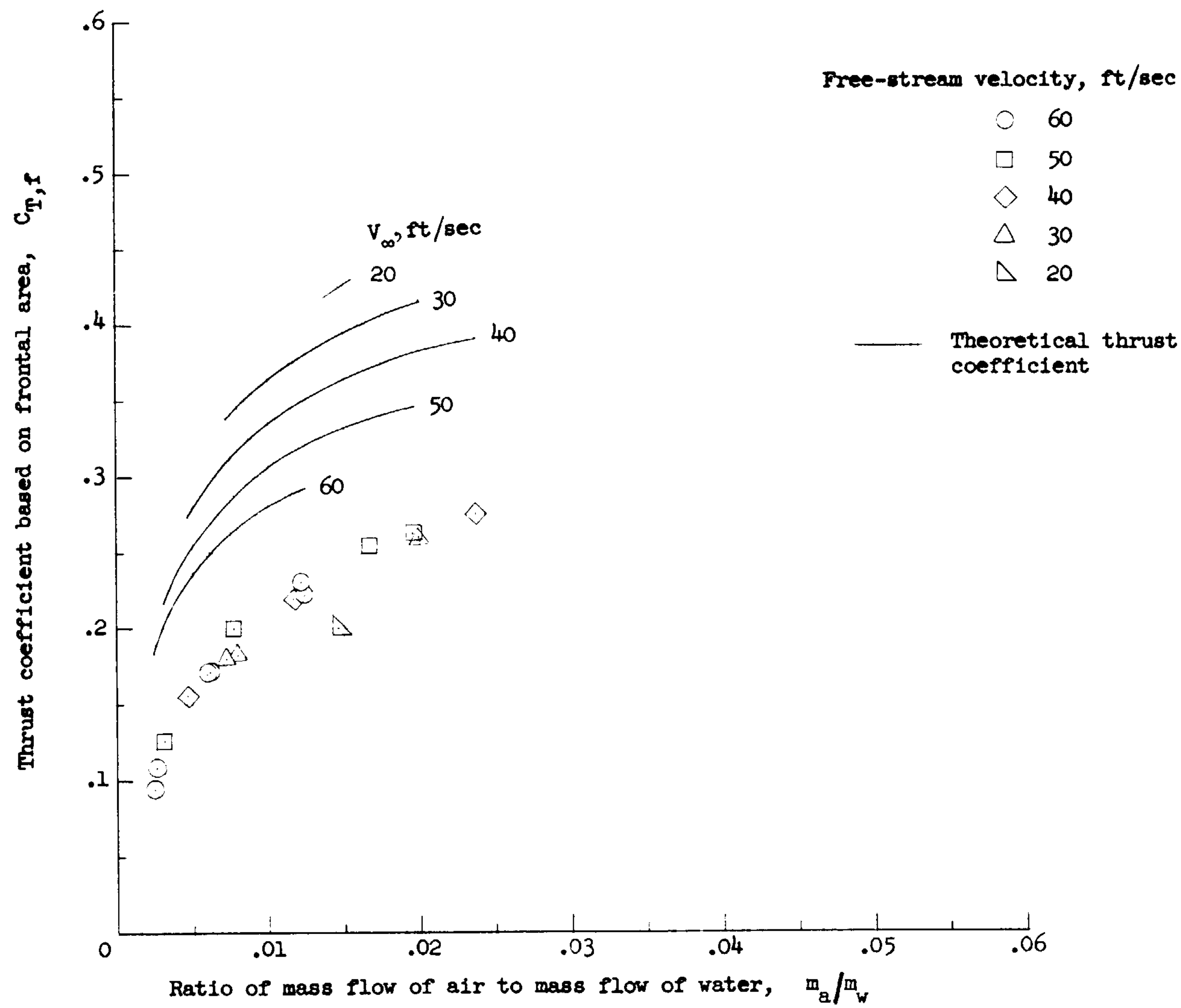

Figure 11.- Comparison of theoretical and measured thrust coefficients.

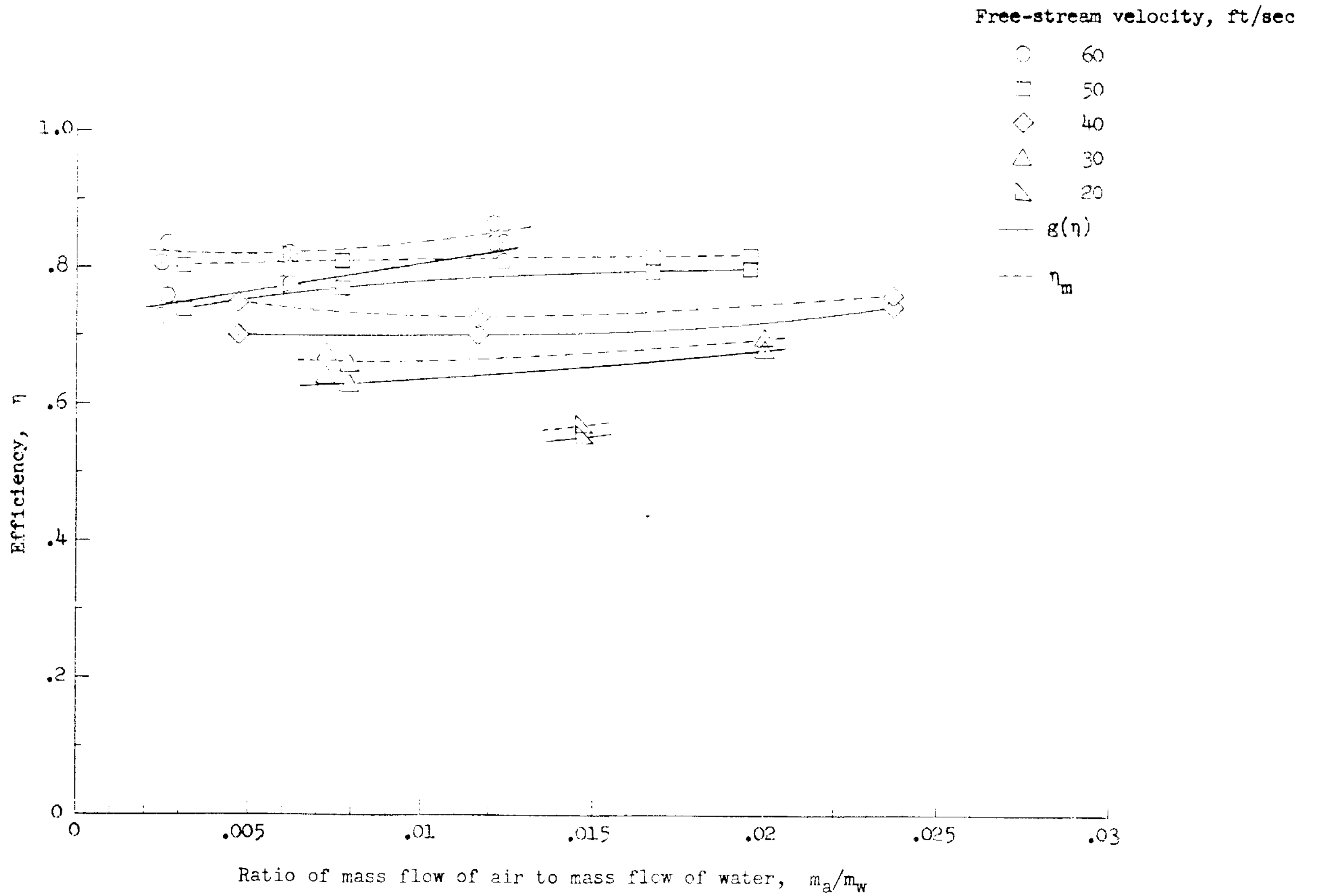

Figure 12.- Overall efficiency and mixing efficiency.

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